



Multilevel approach to lifetime assessment of steam turbines



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ABSTRACT

This paper presents a multilevel methodology for a steam turbine lifetime assessment based on the damage calculation, probabilistic analysis and fracture mechanics considerations. Creep-fatigue damage calculations serve as a basis for evaluating the current lifetime expenditure and for defining additional steps of analysis. The need for the use of probabilistic analysis results from the inherent uncertainty in estimating the lifetime expenditure primarily caused by scatter in material properties. Fracture mechanics considerations are helpful in determining additional safety margins for components containing cracks. This methodology has been illustrated using an example of the lifetime calculations of a high-temperature steam turbine rotor. The calculations were based on the results of 2D numerical simulations performed for steady state and transient operating conditions.

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1. Introduction

Steam turbine components operating at high and highly variable temperatures are exposed to degradation because of creep and thermo-mechanical fatigue processes [1]. These components' lifetime is already limited at the design stage and results from the creep and fatigue characteristics of the material used. The operational wear of these steam turbine components causes the loss of lifetime and decreases the reliability of turbine components, which manifests itself by their increased failure frequency and lower availability.

The problems of lifetime and reliability are essential from the viewpoint of not only avoiding the premature failure of a component before its design life has expired but also extending the service life of power plant equipment beyond its design life [2]. The lifetime extension is driven by economics; nevertheless, it must always be technically sound to ensure the safe operation of turbo-sets.

The basis for any efforts aimed at extending the service life of power turbo-sets is the assessment of the current degree of damage of their components, including the main components of steam turbines. These assessments are performed with the use of metallographic tests and theoretical analyses, which are the necessary elements of a comprehensive assessment of the material state [3]. Theoretical analyses resolve themselves by determining the so-called lifetime fractions and are based on deterministic models in

spite of the large scatter and uncertainty of operation and the material data used for the calculations. This means that the lifetime assessment performed with this method is not definitive and, therefore, requires complementing by using additional analyses such as probabilistic simulations or fracture mechanics calculations.

A lifetime assessment of power plant components operating at creep-fatigue conditions is usually performed using the life fraction rule introduced by Robinson [4] and Taira [5] and recommended by industrial standards, e.g. ASME Code [6] or TRD 508 [7]. In this method, fatigue damage is determined by a cyclic fraction and creep damage is determined by a time fraction, which after summation result in total creep-fatigue damage. Instead of a time fraction, a ductility exhaustion rule is also used for the creep damage calculation [8]. An extensive investigation of both rules was conducted by Colombo [9], who performed thermo-mechanical fatigue experiments on plane, notched and component feature specimens and compared damage results with the predictions of the time fraction and ductility exhaustion rule. The outcome was that both methods resulted in safe predictions for all thermal fatigue tests. Recently, Cui and Wang [10] introduced two lifetime estimation models for steam turbine components under thermo-mechanical creep-fatigue loading. The first model is a phenomenological model developed as an extension of the generalized damage accumulation rule, while the second model is derived from a unified visco-plastic model with incorporated isotropic damage. Both models predicted the creep-fatigue lives within a scatter band of factor two, but the first model resulted in significantly better predictions, resting on the conservative side. Carragher *et al.* [11] presented a lifetime assessment methodology for power plant headers consisting of a

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cyclic visco-plasticity material model and a multi-axial critical-plane implementation of the Ostergren fatigue indicator parameter. Cracking directions predicted by this model are consistent with reported in-service cracking.

The purpose of this paper is to present a methodology for a steam turbine lifetime assessment based on the deterministic damage calculation, probabilistic simulations and fracture mechanics considerations. The advantage of this method is its ability to estimate the probability of creep-fatigue crack initiation when lifetime predictions are based on minimum material property data. This approach is commonly used in engineering practice and is characterized by a high level of conservatism. Moreover, the method enables damage assessment and residual life prediction to be performed based on not only creep-fatigue calculations but also two additional factors: the probability of crack initiation and microstructural damage. As an illustration, lifetime calculations for a high-temperature steam turbine rotor performed with both deterministic and probabilistic methods are presented.

2. Lifetime assessment methodology

The general chart of the lifetime assessment process used in practice is shown in Fig. 1. The process starts with two nearly independent routes: theoretical calculations and material testing [12]. Theoretical calculations aim at evaluating creep-fatigue damage, assuming minimum material properties and calculating failure probability based on the damage computed with the deterministic model and material property data scatter. Material testing aims at revealing surface and volumetric cracks and at determining microstructure damage by using replica or microscopic tests. While crack existence testing on the component surface is performed on all accessible surfaces, the microstructure damage and volumetric crack tests are performed at most critical locations known from theoretical calculations. Damage calculations are used not only to determine critical areas for testing but also to provide very important information on:

- damage mechanism domination at a given location,
- current damage for the worst material properties, and
- the rate of damage accumulation.

This information is useful when defining recommendations regarding the component repair, inspection intervals or modification of operating conditions. The contribution of creep and fatigue damage in total damage is also important from the point of view of failure probability. For a given total damage, the contribution of creep and fatigue damage influences failure probability.

When cracks are not found, a component assessment is performed on the basis of the damage calculation, failure probability and microstructure damage, and the residual lifetime is determined. In case cracks are found in the component, fracture mechanics assessment can be performed to determine safe operation time with propagating crack. Such assessment is not always possible and depends on the damage mechanism and component type. Fracture mechanics calculations are only useful when failure is crack propagation-controlled [13].

For initiation-controlled failures, when the time to crack initiation is considerably longer than the time required for the crack to grow to a critical size or a critical crack size is below the limit of detection, fracture mechanics analyses and conventional non-destructive examination techniques serve no useful purpose. In these circumstances, the existence of a macroscopic crack is, in most cases, a sign of useful life exhaustion, and the crack has to be removed or the component retired.

Failure is initiation controlled when the material is very brittle and when the critical crack size so small, it is below the limit of detection for conventional non-destructive examination techniques. Additionally, high stresses may reduce the critical crack size below the level of detection. If a component has a thin cross section, the remaining ligament can be so small that the crack propagation is of no importance. Moreover, the crack propagation rate can be so high that even with a large critical crack size, once the cracks initiates, it reaches the critical size rapidly. Thus, it is very important to perform an investigation by calculation and material analysis whether the failure is initiation or propagation controlled and to evaluate properly the critical crack size.

The existence of cracks is not always considered as a sort of damage that is dangerous for component safety because the crack growth rate may decrease with an increase in crack size, and crack propagation may stop. This phenomenon is known as crack arrest and can occur, for example, in turbine casings where thermal

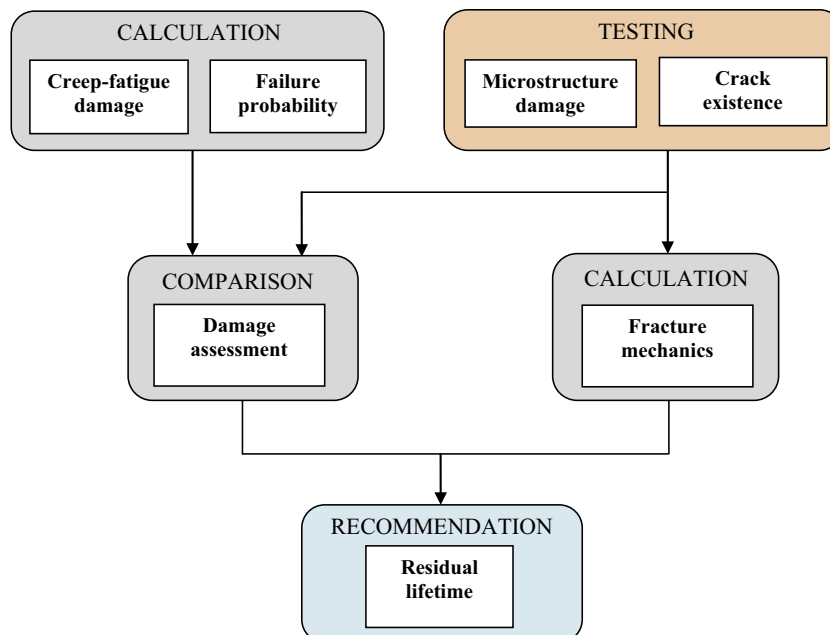


Fig. 1. General chart of lifetime assessment process.

fatigue cracking occurs. Experimental evidence of this phenomenon was provided by Skelton and Nix [14], while the background theory was discussed in detail by Meshii and Watanabe [15,16].

3. Total damage calculation by deterministic approach

Once the design lifetime of a turbine has expired, the question arises for whether it can be safely operated, and if so, for how long and with which conditions? These questions can be answered after analysing the history of the turbine operation and assessing the lifetime expenditure because the state of material of the individual components after 100,000 or 200,000 operating hours depends, first of all, on the manner of operation. For this sake, it is necessary to perform stress analyses for the main turbine components, resulting in a theoretical (computational) damage of material. Estimation of the degree of damage on the basis of operating time is only insufficient if it does not account for fatigue damage, depending on the number of start-ups from various initial thermal conditions.

Three types of information are required for the remaining lifetime assessment of a turbine component [3]:

- current degree of damage,
- damage accumulation rate, and
- degree of damage causing failure.

The current degree of damage (lifetime usage) of steam turbine components is determined on the basis of the operating data and mathematical model assumed, describing the process of creep and fatigue damage accumulation. For the lifetime assessment of steam turbine components, stress corrosion cracking and corrosion fatigue are important issues, but they are not included in the approach and are discussed elsewhere [17]. Theoretical damage due to creep and thermal fatigue is determined with the help of the lifetime fractions summation method. In steam turbine practice, a linear model of damage in the form of the linear rule of damage summation is generally used. This rule states that total damage is the sum of creep and fatigue damage, and can be written as follows [12]:

$$Z = Z_B + Z_N, \quad (1)$$

where Z_B denotes creep damage and Z_N denotes fatigue damage.

The magnitude of total creep damage Z_B depends on the duration time t of a load at a given temperature and on the magnitude of damage $1/t_B$ in unit time, where t_B is the time to rupture. Its value is derived from the minimum creep curve. According to the linear rule of creep damage accumulation (Robinson's hypothesis), a summation is performed proportionally to the load duration time. For a set of stresses and temperatures at steady-state operation, the creep damage can be calculated from the following formula [18]:

$$Z_B = \sum_{i=1}^r \frac{t_i}{t_{Bi}}, \quad (2)$$

where r stands for the number of stress and temperature levels.

The magnitude of total fatigue damage Z_N depends on the number of load cycles n and the magnitude of damage $1/N$ in each load cycle, where N is the number of cycles to crack initiation. Its value is derived from the minimum LCF curve. According to the linear rule of fatigue damage accumulation (Palmgren's–Miners' hypothesis), a summation is performed proportionally to the number of cycles. For a stress spectrum, which in real turbine operation conditions consists of start-ups from cold, warm and hot states, and from load changes, the fatigue damage is calculated from the following formula [12]:

$$Z_N = \sum_{i=1}^q \frac{n_i}{N_i}, \quad (3)$$

where q denotes the number of stress levels.

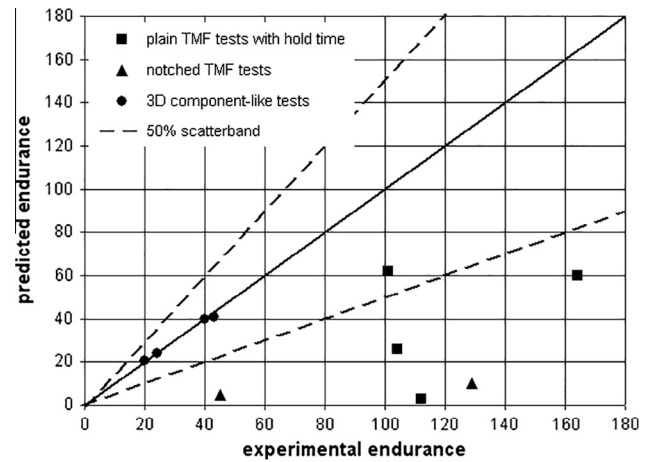


Fig. 2. Comparison between measured and predicted lives from creep-fatigue damage assessment using time fraction rule [9].

The linear rule of damage summation has been validated for 1CrMoV rotor steels by Colombo [9], who performed thermal fatigue tests on uniaxial and component feature specimens under service-like conditions. The linear rule resulted in conservative endurance predictions for all specimens but with significantly lower scatter in 3D component-like tests compared with plain and notched thermo-mechanical tests. In the latter tests, the scatter of calculated endurance was considerably lower when the ductility exhaustion rule was used for the creep damage calculation. However, as it is seen from Fig. 2, in 3D component-like tests simulating high temperature steam turbine rotor operation under real conditions, the time fraction rule used for the creep damage calculation resulted in more accurate endurance predictions compared with the ductility exhaustion rule. Very good accuracy of the linear damage summation rule using the time fraction procedure for the creep damage calculation fully justifies its use in steam turbine component damage assessments.

Theoretically, the computational lifetime is exhausted when the total damage Z reaches one. This critical value, i.e. the value of exhaustion causing failure, is assumed for fatigue cycles, and it denotes crack initiation. Further operation from the instant of crack initiation to the instant of total rupture is associated with an increased risk of failure. In the case of creep damage, the moment that micro-cracks appear coincides usually with the onset of tertiary creep, which is typically reached after approximately 75% of the time to rupture t_B . Thus, for steam turbine components where creep and fatigue processes occur in parallel, the critical value of 0.75 is assumed [12,19]. Once this critical value D is reached, an increased risk of cracking occurs and failure possibility increases, and the turbine is further operated with a gradually decreasing safety margin. The end of theoretical service life is a warning signal indicating the necessity of initiating a systematic test program and applying extended supervision of the operated component.

4. Sources of uncertainty in the deterministic approach

Many years of experience in numerous laboratories show that the scatter of creep properties for most high-temperature creep resisting steels is in the range of $\pm 20\%$ of the mean values of the creep rupture strength and creep strain strength. The corresponding scatter in time reaches several hundred percent [20]. This approximate character of experimental data and the resulting limitations in full use of extrapolated values result from:

- heterogeneity of the material and differentiation in chemical composition, structure and heat treatment of the same materials at different melts,

- errors in creep tests (control and measurement of temperature and stresses),
- differentiation in operating conditions and the influence of environment compared to laboratory parameters,
- existence of physical variation of phenomena occurring in short-term tests and during service, and
- impact of technological and design factors on the material lifetime.

For example, creep tests are performed in air at elevated temperatures where oxidation effects adversely affect the strength of steels. In actual conditions, the oxidation effect with reference to a large section of the rotor, for example, may be negligible [2]. Typical examples of the scatter band of creep rupture strength for cast steel used for casings are shown in Fig. 3.

A similar trend is observed in thermal fatigue characteristics of turbine steels. A typical scatter band of the total strain amplitude in low-cycle fatigue is on the level of $\pm 10\%$ of the strain mean value and results in a considerably higher scatter for the number of cycles to crack initiation.

The above factors make the lifetime assessment, based on the deterministic model and minimum strength properties, very conservative.

An additional factor introducing uncertainty into the deterministic model is the averaging of operating conditions. The lifetime

studies are performed for representative start-ups from a cold, warm and hot state, selected from a spectrum of starts the turbine has undergone. An example of the analysis taking into account the scatter of operating parameters in determining stresses in 200 MW turbine stop valves is presented in Ref. [21]. For newer turbines equipped with modern data acquisition systems or dedicated lifetime monitoring systems, the problem of different operating conditions can be solved by processing all operation history recorded by the system, calculating temperatures and stresses, and observing the resulting creep and fatigue damage. A dedicated methodology was developed by the author at ALSTOM [22], which ensures considerably more accurate lifetime predictions than the approach based on representative starts and eliminates the problem of inaccurately defined operating conditions.

5. Probabilistic method of lifetime assessment

Based on the above reasons, a probabilistic method has been proposed as described in [23]. The probabilistic methodology of the lifetime assessment is based on the laboratory tests results, where the scatter of creep and fatigue properties of turbine steels can be described by log-normal distribution. Based on the data contained in the material database developed by ALSTOM, it can be assumed that the minimum and mean values of the creep rupture strength R_m and strain amplitude ε_a are related by a constant factor f_{Rm} and f_ε (see Fig. 4a and b).

The adequate relationships are as follows:

$$R_m(P = 50\%) = f_{Rm} R_m(P = p_{\min}), \quad (4)$$

$$\varepsilon(P = 50\%) = f_\varepsilon \varepsilon(P = p_{\min}). \quad (5)$$

To determine the unknown factors f_{tB} , f_N , one can write on the basis of geometrical relations from creep and fatigue curves:

$$\frac{d \log(R_m)}{d \log(t_B)} = - \frac{\log(f_{Rm})}{\log(f_{tB})}, \quad (6)$$

$$\frac{d \log(\varepsilon)}{d \log(N)} = - \frac{\log(f_\varepsilon)}{\log(f_N)}. \quad (7)$$

After transformations, one obtains a formula for the time to rupture factor f_{tB}

$$f_{tB} = 10^{-\frac{d \log t_B}{d \log R_m} \log f_{Rm}},$$

and factor f_N for the number of cycles to crack initiation

$$f_N = 10^{-\frac{d \log N}{d \log \varepsilon} \log f_\varepsilon}. \quad (9)$$

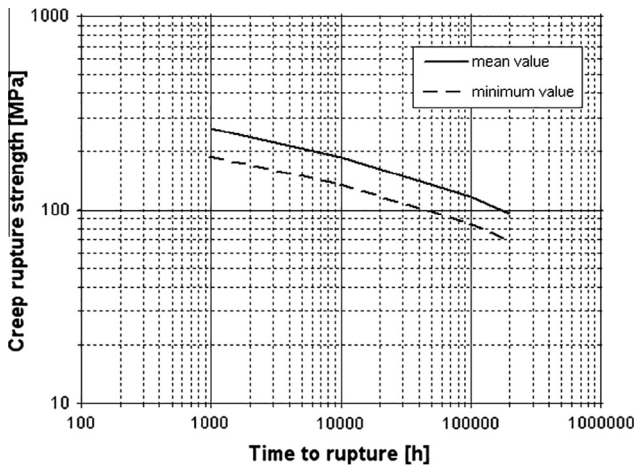


Fig. 3. Creep rupture strength of a typical CrMoV cast steel.

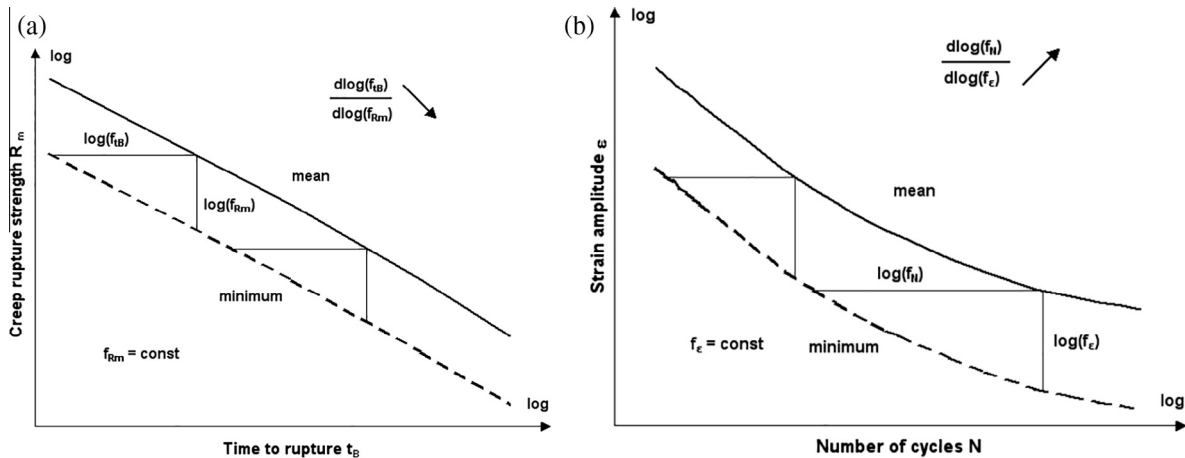


Fig. 4. Determination of standard deviation for time to rupture (a) and number of cycles to crack initiation (b).

In the above relationships, the factors describing creep rupture strength and strain amplitude, and the slopes of the creep and fatigue curves, are known from creep and fatigue characteristics and their scatter.

Because

$$\log t_B(P = 50\%) = \log t_B(P = p_{\min}) + \log f_{tB}, \quad (10)$$

$$\log N(P = 50\%) = \log N(P = p_{\min}) + \log f_N, \quad (11)$$

then

$$t_B(P = 50\%) = f_{tB} t_B(P = p_{\min}), \quad (12)$$

$$N(P = 50\%) = f_N N(P = p_{\min}). \quad (13)$$

On this basis, one can calculate the standard deviation for the time to rupture

$$\sigma_{tB} = \frac{\log \left(\frac{t_B(P=50\%)}{t_B(P=p_{\min})} \right)}{x(F(x) = p_{\min})}, \quad (14)$$

and the standard deviation for the number of cycles to crack initiation

$$\sigma_N = \frac{\log \left(\frac{N(P=50\%)}{N(P=p_{\min})} \right)}{x(F(x) = p_{\min})}. \quad (15)$$

In formulae (14) and (15), x is a value of the random variable X for which the cumulative distribution function F of the standardized normal distribution [24] is equal to the minimum value of probability p_{\min} .

Having the standard deviation, one can easily calculate values of the time to rupture and the number of cycles to crack initiation for any value of probability p using the following formulae:

$$\log \left(\frac{t_B(P = p)}{t_B(P = 50\%)} \right) = -x(F(x) = p) \sigma_{tB}, \quad (16)$$

$$\log \left(\frac{N(P = p)}{N(P = 50\%)} \right) = -x(F(x) = p) \sigma_N. \quad (17)$$

According to the linear rule of damage accumulation used in the deterministic approach, it can be written as:

$$Z = \frac{t}{t_B(P = p_{\min})} + \frac{n}{N(P = p_{\min})}. \quad (18)$$

For a failure probability P , the degree of damage $Z = 1$ can be written as:

$$Z(P) = \frac{t}{t_B(P = p)} + \frac{n}{N(P = p)} = 1. \quad (19)$$

Taking into account that for fatigue damage the permissible limit D_N is different from the limit D_B for creep damage, Eq. (19) can be rewritten as follows:

$$Z(P) = \frac{1}{D_B} \cdot \frac{t}{t_B(P = p)} + \frac{1}{D_N} \cdot \frac{n}{N(P = p)} = 1. \quad (20)$$

For a given operation time t_{op} , the number of start-ups n_{op} is known, which allows one to express the time to failure as:

$$t \left(\frac{1}{D_B t_B(P = p)} + \left(\frac{n_{op}}{t_{op}} \right) \cdot \frac{1}{D_N N(P = p)} \right) = 1. \quad (21)$$

Taking into consideration that

$$Z_B = \frac{t_{op}}{t_B(P = p_{\min})}, \quad (22)$$

$$Z_N = \frac{n_{op}}{N(P = p_{\min})}, \quad (23)$$

and distinguishing cold, warm and hot cycles, the following formula is obtained

$$t = \frac{t_B(P = p_{\min})}{\left[\frac{t_B(P=p_{\min})}{t_B(P=p)} \cdot \frac{1}{D_B} + \sum_{i=Z,C,G} \left(\frac{Z_{N,i}}{Z_t} \cdot \frac{N(P=p_{\min})}{N(P=p)} \cdot \frac{1}{D_N} \right) \right]}. \quad (24)$$

The above formula describes the operation time t required to reach the failure probability p . Failure here denotes the initiation of fatigue cracks or the beginning of tertiary creep.

6. Crack analysis by fracture mechanics

Creep-fatigue damage is limited to a critical value assumed as a point of macroscopic fatigue or creep crack initiation. The time from crack initiation to component failure may be in some circumstances long enough to justify the additional operation of a cracked component with an acceptable safety margin when performing periodic inspections.

To perform the remaining lifetime assessment analysis of components containing cracks, three types of information is required [25]:

- initial crack size
- crack growth rate
- critical crack size

Macroscopic cracks can be detected and dimensioned using conventional non-destructive testing techniques such as ultrasonic, eddy-current or magnetic-particle tests.

Crack analysis can be further performed to evaluate the crack growth rate using fracture mechanics methods. For steam turbine components, the methodology for crack growth analysis includes:

- time-independent fatigue
- time-dependent creep crack growth
- creep-fatigue interactions

At lower temperatures where creep processes are practically not present or creep damage is not significant or at a high frequency of thermo-mechanical loading, the crack growth is solely governed by fatigue. Fatigue crack growth can be described by the Paris law [26]:

$$\frac{da}{dn} = C' \Delta K^m \quad (25)$$

where a is a crack size and ΔK is the stress intensity factor range, while C' and m denote material constants depending on temperature and frequency.

At high operating temperatures and a low frequency of fatigue loading, the growth of cracks depends on the crack-tip driving force C^* through the relationship [13]:

$$\frac{da}{dt} = b(C^*)^l \quad (26)$$

where b and l are material constants.

In case of the co-existence and interaction of creep and fatigue, Ammirato has derived the following expression for crack growth under creep-fatigue conditions [27]:

$$\frac{da}{dn} = C' \Delta K^m + b(C^*)^l t_h + C \Delta K^x t_h^z \quad (27)$$

where x and z are material constants and t_h is a hold time

Critical crack size is evaluated by considering the failure mechanism leading to component rupture or burst. It is always a function of the appropriate material property and component geometry. For steam turbine components, critical crack sizes are evaluated for the following failure mechanisms:

- brittle fracture
- plastic overloading
- high-cycle fatigue

Brittle fracture occurs when the crack size reaches its critical value and is evaluated based on the material fracture toughness. In thin-walled components or at small cross sections, failure can be caused by the plastic overloading of the remaining ligament before the crack reaches its critical size and is evaluated based on the fracture toughness. Failure may also be caused by high-cycle fatigue if a crack grows due to creep or low-cycle fatigue and reaches the threshold value for high-cycle fatigue crack propagation. If all failure mechanisms are present, the critical crack size is determined as the minimum of the critical crack sizes determined for each mechanism:

$$a_{cr} = \min \{a_{cr}^{BF}, a_{cr}^{PO}, a_{cr}^{HCF}\} \quad (28)$$

In this expression, *BF* is the abbreviation for brittle fracture, *PO* is the plastic overload and *HCF* is high-cycle fatigue.

7. Computational example

The methodology described above was applied to a 50 MW steam turbine rotor. The rotor is highly loaded by thermal and mechanical stresses because it is operated at c.a. 500 °C and 3000 rpm and is, therefore considered as a critical turbine component.

For a deterministic estimation of the degree of rotor damage, thermal and strength computations using the finite element method were performed. Rotor temperatures were calculated using a 2D axisymmetric model of a complete rotor extending axially from journal to journal and radially outward to positions corresponding to the discs' outer diameters. The temperature distribution in the rotor is governed by the temperature of the secondary steam flows through the glands and in the spaces between the discs and adjacent diaphragms and by the heat transfer coefficients between the steam and the rotor surface. Heat transfer coefficients were calculated for forced convection allowing for the axial and tangential steam velocity in the enclosed spaces between the stationary components and the rotor. The radially outermost surfaces of the model were assumed to be insulated. The derived boundary conditions were applied to the finite element model, and solutions for the steady state and transient temperature distribution were obtained.

Rotor temperature distributions were used as thermal loading in the structural model. Mechanical loads in the form of rotational body forces and steam pressures were applied to the rotor model. The rotor was re-strained at one end to prevent axial movement. Temperature dependent material properties were used in all simulations.

In transient analyses simulating start-ups and shutdowns, the linear-elastic material model was used. Equivalent strain amplitudes required for the evaluation of fatigue damage due to start-up-shutdown cycles were calculated based on von Mises stress amplitudes using Neuber's correction rule:

$$\varepsilon_a = \frac{\sigma_a^2}{\sigma_y E} \quad (29)$$

where

- ε_a – strain amplitude,
- σ_a – Mises stress amplitude,
- E – Young modulus,
- σ_y – cyclic yield stress.

The fatigue damage calculated with the elastic material model is considered as conservative. Elastic stress calculations are performed when a larger number of cycles is to be computed, and

the use of the elastic-plastic material model would be very time-consuming and impractical. When the lifetime assessment of new components is performed, the linear elastic-plastic material model with kinematic hardening is employed. A method for correlating a large number of strain amplitudes obtained with Neuber's correction using strain amplitudes calculated with the elastic-plastic material model was developed by the author and described in the internal instruction [22].

For the creep damage assessment, steady-state creep calculations are performed using the viscoelastic material model. Creep strain is calculated using a characteristic strain model proposed by Bolton [28] and is expressed in the form:

$$\varepsilon_c = \frac{\varepsilon_\chi}{\sigma_R / \sigma - 1}, \quad (30)$$

where ε_c is the creep strain, ε_χ is a characteristic creep strain being a material constant, σ_R is the creep rupture strength, and σ is the current stress. For varying stress, the strain hardening rule is applied. The model was successfully used for analysing both stationary [29,30] and rotating components [31].

As a result of finite element calculations, 2D temperature and stress distributions for various start-ups and in steady-state conditions were obtained. On this basis, the most highly loaded locations were identified. Moreover, stresses and temperatures required for lifetime calculations were evaluated. An example of the temperature and stress distribution in the rotor during warm start-up is shown in Fig. 5, where critical locations are also marked. They are located in the area of the transition radius between the first stage disc and balance piston (location 1) and at the rotor bore surface beneath the first stage disc (location 2). This section of the rotor has the highest temperatures, resulting from high steam temperature and giving rise to creep damage. Moreover, steam temperature rates during start-ups are largest in the inlet sections, which results in higher metal temperature gradients, which, together with geometrical notches (location 1), generate large thermal stresses and cause fatigue damage.

Temperature and stress variations at rotor critical locations during warm start-up are shown in Fig. 6. Higher transient stress occurs at location 1, which is heated directly by the hot steam and results in higher metal temperature rates compared to the rotor bore (location 2). Consequently, high thermal stresses reaching 670 MPa are generated there, while the rotor bore experiences significantly lower stress at approximately 350 MPa. Transient temperature and stress variations form a basis for calculating fatigue damage, while steady-state temperature and stress distributions were used to calculate creep damage.

For the lifetime calculations, the following operating data were assumed:

- total operation time – 203,225 h
- number of cold starts – 324
- number of warm starts – 121
- number of hot starts – 335

The lifetime expenditure was calculated with the help of the method described in Section 3 and for the operating data given above. The results of the computations are summarized in Table 1. It is seen from the table that at location 1, a significant amount of creep and fatigue damage has been accumulated, which, in total, results in a very short remaining life of the rotor on the level of 13,000 h only. In contrast, at location 2, creep damage is dominant and fatigue damage is negligible. The rotor remnant life at this location exceeds 164,000 h, and the rotor bore is not life-limiting.

As part of the rotor lifetime assessment, non-destructive tests and metallurgical investigations were performed. Non-destructive examination included visual, eddy-current, magnetic-particle and

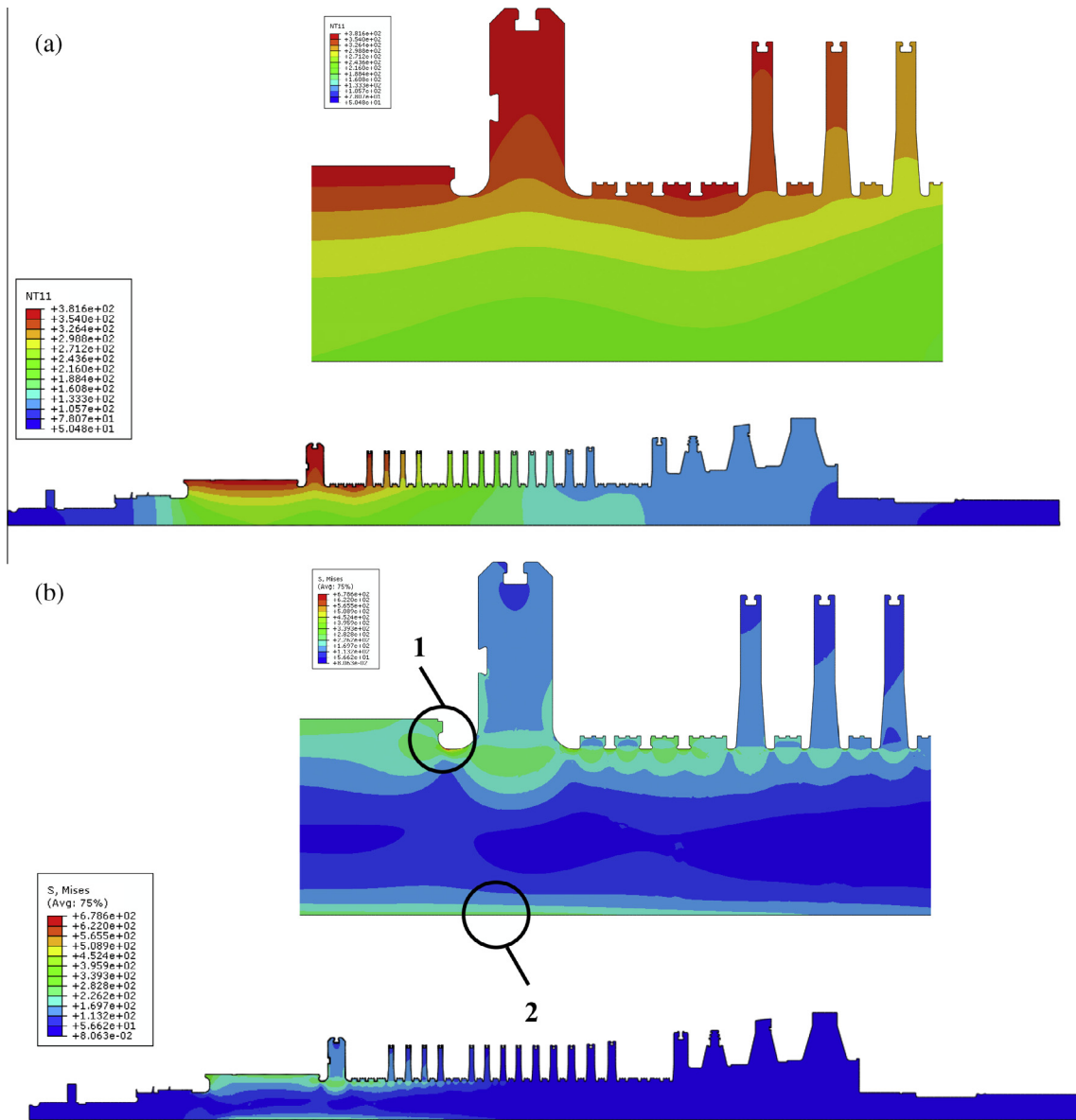


Fig. 5. Temperature (a) and von Mises stress (b) distribution after 27 min of warm start-up.

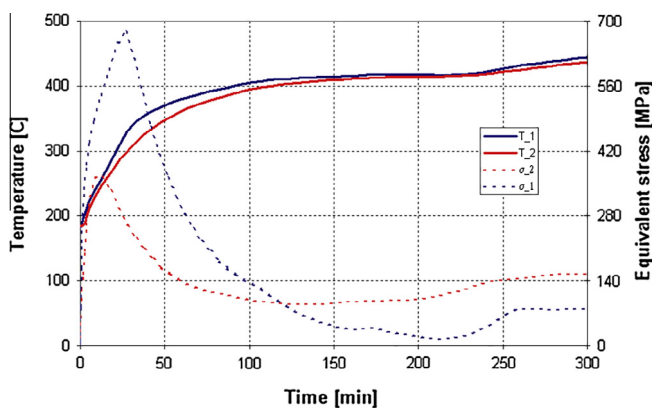


Fig. 6. Temperature and equivalent stress variation at rotor for two critical locations during warm start-up.

ultrasonic tests, while metallurgical investigations consisted of hardness tests and microstructure examinations. The results con-

Table 1
Lifetime expenditure of the rotor.

Location	Fatigue life expenditure, Z_N (-)			Creep life expenditure, Z_B (-)	Total life expenditure, Z (-)	Remaining lifetime, t_R (hours)
	Cold	Warm	Hot			
1	0.229	0.054	0.016	0.406	0.705	13,114
2	0.003	0.001	0.003	0.460	0.468	164,670

firmed that location 1 has the highest creep damage on the rotor external surfaces because the lowest hardness was found and the highest microstructure damage was confirmed by medium size carbides in grains and bulky carbides on grain boundaries. A photo of the microstructure from the microscopic test is shown in Fig. 7. No creep-fatigue cracking was found on the rotor surface or in the central bore, which is in agreement with theoretical damage predictions.

On this basis, calculations of the probability for crack initiation in the rotor were performed using the methodology described Section 5. The results of the calculations are presented in Figs. 8 and 9.

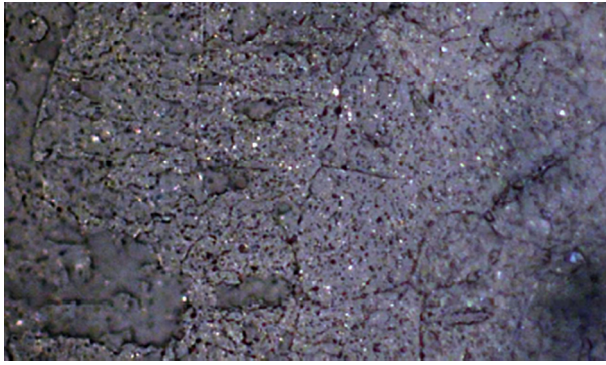


Fig. 7. Material microstructure at location 1 in 1000 times magnification.

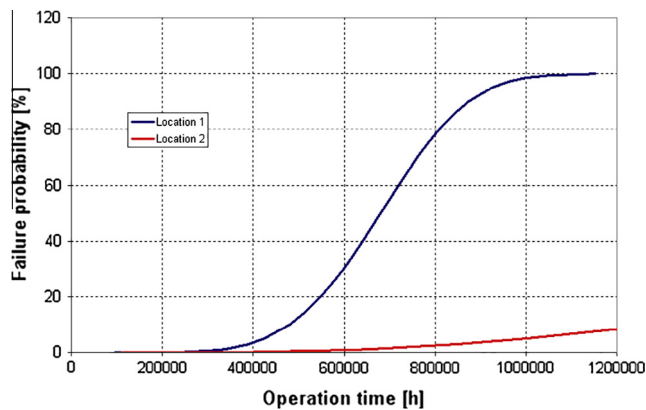


Fig. 8. Failure probability at locations 1 and 2.

At location 1, the current degree of damage is approximately 0.7, which is very close to the critical value, and the failure probability is 0.05%, while at location 2, the failure probability corresponding to damage 0.468 is 0.005%. In the former case, this means that in 5 out of 10,000 rotors in operation, cracks would develop, whereas in the latter case, in 5 out of 1,000,000 cracks would develop.

The example given illustrates a more intensive increase of failure probability for the rotor and a divergence of the failure probability curves for both locations. A clearly visible increase of probability at location 1 is observed after c.a. 300,000 h and after 400,000 h of operation, a relatively high probability of 3% is exceeded. Assuming a certain critical level of failure risk, it is possible to determine a further allowable operation time with increasing probability of rotor failure [32]. It must always be remembered that failure is defined as fatigue crack initiation or the onset of tertiary creep and not as total rupture. The growth of fatigue cracks on the rotor surface can be monitored by regular inspections, while monitoring the operation in the tertiary creep regime is very risky. Taking into account the theoretically calculated remaining life of the rotor equal c.a. 13,000 h (critical location 1) supplemented by the additional results on failure probability and microstructure damage, the rotor remaining life was extended to c.a. 100,000 h. Thus, the application of the multilevel approach provided broader knowledge about the rotor condition and made it possible to extend its life by c.a. 87,000 h, assuming an unchanged future operation regime.

It should be noted that failure probability is not only a function of time and total damage. Creep and low-cycle fatigue data have different scatter bands and, usually, different probabilities of minimum values are assumed. The scatter of creep rupture stress is larger than the scatter of strain amplitude, and, similarly, the

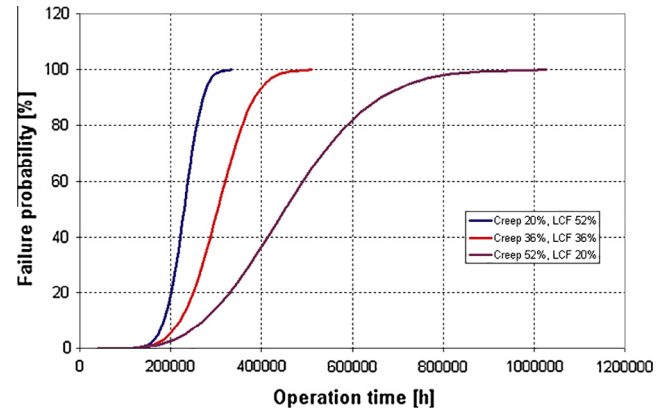


Fig. 9. Effect of creep and fatigue damage content on failure probability.

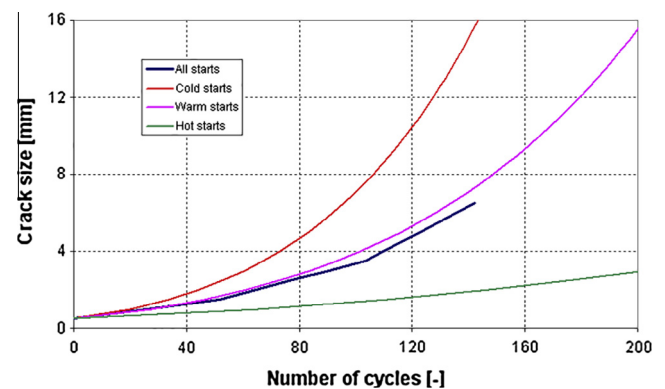


Fig. 10. Crack propagation from rotor surface (location 1) due to thermal fatigue and creep.

probability of the minimum value of creep rupture stress is larger than that of the minimum value of strain amplitude. Consequently, the failure probability will be dependent on the contribution of creep and fatigue damage as well, and the proposed methodology allows for considering this effect.

A parametric analysis has been performed to investigate the effect of different contributions of creep and fatigue damage on total failure probability. Fig. 9 shows the failure probability curves for different contributions of creep and fatigue damage after 100,000 h of operation. The higher the creep damage contribution in total damage is, the lower the failure probability is. The characteristic is very sensitive to the contribution of creep and fatigue damage and time. For example, after 300,000 h the failure probability is 98% for an initial creep damage equal to 0.2 and drops by half to 49% for the initial creep damage contribution of 0.36. It drops further to 15% when the creep damage is dominant and amounts to 0.52. The differences are less visible for shorter times, when total damage is closer to the critical value and the individual failure probabilities are still very low.

The rotor surface at location 1 is the most highly exhausted area, and the crack initiation probability will quickly grow with time. Additionally, the potential crack propagation will be fastest here because the stress intensity factor determined by the maximum stress is largest in this region. The crack growth due to creep-fatigue can be evaluated using Eq. (27) for calculated temperature and stress distributions during start-ups and at steady-state operation.

The results of the circumferential crack growth computations at location 1 are shown in Fig. 10. The fastest crack growth takes place during the cold start-shutdown cycle because transient thermal stresses occurring then are highest. Crack growth per cycle

increases as the crack size increases for all start-up types due to the increasing stress intensity factor for the same maximum stress. For the real start-up spectrum the rotor experiences, the crack size increase will be between that for warm and hot start-ups and slightly below the former. A comparison of the actual crack size with its critical value provides useful information on the existing safety margin and additional safe operation time.

8. Summary

The methodology of the lifetime assessment of steam turbine components presented in this study has considerable practical importance. This method is a multi-level approach using state-of-the-art methods and tools employed for temperature, stress and damage calculations. A deterministic damage assessment is a primary level in theoretical analyses and serves as a starting point for additional considerations. Additional levels of analyses that use probabilistic and fracture mechanics methods provide very useful information for component damage assessments and remaining life estimations.

The presented methodology possesses numerous advantages. It has been used in daily practice by lifetime assessment engineers and is proven by numerous lifetime assessment studies.

The approach described in this paper is implemented in practical engineering tools, and its usefulness was illustrated with an example of its practical application to a steam turbine rotor.

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